

AD-A062 266 ARMY ARMAMENT RESEARCH AND DEVELOPMENT COMMAND ABERD--ETC F/G 21/2
ON CALCULATING IGNITION OF A PROPELLANT BED.(U)
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PROPELLANT BED.

10 Carl W. Nelson

11 September 1978

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REPORT DOCUMENTATION PAGE		READ INSTRUCTIONS BEFORE COMPLETING FORM										
1. REPORT NUMBER MEMORANDUM REPORT ARBRL-MR-02864	2. GOVT ACCESSION NO.	3. RECIPIENT'S CATALOG NUMBER										
4. TITLE (and Subtitle) On Calculating Ignition of a Propellant Bed		5. TYPE OF REPORT & PERIOD COVERED										
		6. PERFORMING ORG. REPORT NUMBER										
7. AUTHOR(s) Carl W. Nelson		8. CONTRACT OR GRANT NUMBER(s)										
9. PERFORMING ORGANIZATION NAME AND ADDRESS US Army Ballistic Research Laboratory ATTN: DRDAR-BLP Aberdeen Proving Ground, MD 21005		10. PROGRAM ELEMENT, PROJECT, TASK AREA & WORK UNIT NUMBERS RDT&E 11161102AH43										
11. CONTROLLING OFFICE NAME AND ADDRESS US Army Armament Research & Development Command US Army Ballistic Research Laboratory ATTN: DRDAR-BL Aberdeen Proving Ground, MD 21005		12. REPORT DATE SEPTEMBER 1978										
13. MONITORING AGENCY NAME & ADDRESS// different from Controlling Office)		13. NUMBER OF PAGES 38										
		14. SECURITY CLASS. (of this report) Unclassified										
		15a. DECLASSIFICATION/DOWNGRADING SCHEDULE										
16. DISTRIBUTION STATEMENT (of this Report) Approved for public release; distribution unlimited.												
17. DISTRIBUTION STATEMENT (of the abstract entered in Block 20, if different from Report)												
18. SUPPLEMENTARY NOTES												
<p style="text-align: right;">DDC- REF ID: A65191 DEC 19 1978 B</p>												
19. KEY WORDS (Continue on reverse side if necessary and identify by block number) <table style="width: 100%; border-collapse: collapse;"> <tr> <td style="width: 33%;">Ignition</td> <td style="width: 33%;">Computer Codes</td> </tr> <tr> <td>Solid Propellants</td> <td>Heat Conduction</td> </tr> <tr> <td>Flamespreading</td> <td></td> </tr> <tr> <td>Internal Ballistics</td> <td></td> </tr> <tr> <td>Guns</td> <td></td> </tr> </table>			Ignition	Computer Codes	Solid Propellants	Heat Conduction	Flamespreading		Internal Ballistics		Guns	
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Guns												
20. ABSTRACT (Continue on reverse side if necessary and identify by block number) (meg) <p>Various criteria for ignition of solid propellants are considered for use in gun ballistics calculations. Experimental data are examined and extrapolated for use in the NOVA computer code. DeLuca's laser ignition data are considered most applicable. Calculated flamespreading rates with a surface temperature criterion agree better with measured rates than those calculated with ignition delay criteria from radiant flux experiments. Goodman's integral method is shown satisfactory for use in a linearly rising heat flux.</p>												

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LIST OF SYMBOLS.

- c - Specific heat of solid.
E - Energy absorbed at ignition.
 \bar{q} - Average heat flux defined by Equation (5).
q - Heat flux.
r - Regression rate of solid.
 t_{ig} - Ignition delay.
t - Time.
 T_s - Surface temperature of solid.
 T_o - Initial temperature.
V/S - Volume-to-surface ratio for a grain.
 α - Thermal diffusivity of solid.
 λ - Thermal conductivity of solid.
 ρ - Solid propellant density.
 ΔT_B - Bulk temperature change.

TO SYNTHESIZED IGNITION SEQUENCES IN A BED OF SOLID PROPELLANT GRAINS

I. INTRODUCTION

It is no secret that it is difficult to calculate the ignition sequence in a bed of solid propellant grains. Computer codes have attempted to describe it by solving one-dimensional equations for the two phase flow. Even if one concedes correctly posed differential equations, there remains the question of posing the auxiliary equations or data for ignition criteria, heat transfer to the grains, transient regression rates, igniter gas flow, and the like.

Application of these computer codes to solid propellant guns has principally focussed on the description of pressure wave development in the gun chamber. More than any other, it has become the standard by which to judge the merit of a code. And well it should because pressure difference is one of the few presently measurable phenomena to compare with prediction. Parametric studies have established that regression rates, bore resistance, and drag, can be varied enough to force agreement between predicted and measured pressures. For the time being, this agreement is taken as a sufficient basis to justify interior ballistics studies of gun behavior as the propelling charge is modified.

There is considerable evidence that the ignition behavior strongly influences the gun's susceptibility to catastrophic overpressure. Describing the ignition requires first describing how the propellant itself makes the transition from heating to combustion. In the gun, the ignition front movement is the progression of the transition event through the bed. The one measure of that movement is the flamespreading rate which is only beginning to be measured. Such measurements will, however, provide another standard by which to judge a code. It is necessary now to examine the details of the ignition and its computation to improve, if feasible, the postulated laws.

This report will address the ignition criterion for the propellant and the heating of the solid to the ignition regime. It will examine the existing experimental ignition data and computational technique of the NOVA computer code with a view to improving the postulated ignition sequence.

II. IGNITION CRITERION

It would be most convenient if the solution of the transient heat conduction in the solid led to an unambiguous condition that marked a sharp transition from endothermic heating to self-sustained combustion. Unhappily such a condition cannot be found by reference to experimental data on ignition of nitrocellulose base propellants. Easily found in the conduction calculation are surface temperature, total energy absorbed, distribution of temperature and energy; of these, none has been uniquely correlated with ignition. Worse, the propellant sees a time-dependent, convective energy source but all the experiments used a constant radiant source. It is nonetheless necessary to somehow apply the data to obtain a usable ignition criterion.

Flamespreading codes to date have used surface temperature or total absorbed energy as a criterion, usually without detailed justification. Gough referenced the theory of Andersen² but used surface temperature for simplicity because "the availability of supporting data favors (it)". It is rather the lack of available data on the parameters of Andersen's theory that precludes its use. Koo and Kuo postulated a sequence in which ablation started at some threshold temperature. The transition to full combustion at the ignition temperature allowed a convective contribution linearly dependent on how near the surface temperature is to the full ignition temperature. This is, in effect, a surface temperature criterion because no other feature is considered. References for the specific temperatures were not reported although they are within the generally accepted limits for ablation and combustion of nitrocellulose base propellants.

Krier⁴ selected a criterion consistent with his continuum mechanics approach by specifying a bulk temperature rise in the solid phase. Although this is, on its face, a total energy absorbed criterion, it cannot be directly compared with ignition experimental data. Ignition energy is reported on a unit surface area basis; the bulk temperature criterion is on a unit mass basis. To compare the two, consider Krier's gun calculation. For a bulk temperature rise ΔT_B , the energy absorbed per unit surface area is

$$E_B = (\rho \Delta T_B) \left(\frac{V}{S} \right) \quad (1)$$

where

ρ = density equal to ratio of bulk to surface density

c = specific heat at constant pressure of solid and

(V/S) = volume to surface ratio

1. P.S. Gough, "Numerical Analysis of a Two-Phase Flow with Explicit Internal Boundaries", ITRC 77-5, Naval Ordnance Station, Indian Head, MD, April 1977.
2. W.H. Andersen, "Model of Transient Ignition to Self Sustained Burning", Comb. Sci. and Tech., 5, p. 75-81 (1972).
3. J.H. Koo and K.K. Kuo, "Transient Combustion in Granular Propellant Beds, Part I. Theoretical Modeling and Numerical Solution of Transient Combustion Processes in Mobile Granular Propellant Beds", BRL Contract Report No. 346, August 1977. (AD #A044998)
4. H. Krier, "Predictions of Flamespreading and Pressure Wave Propagation in Propellant Beds", University of Illinois Technical Report AAE-75-6, July 1975.

It should be noted here that for a given ignition energy, presumably obtainable from experimental data, the bulk temperature assigned depends on the volume to surface ratio of the grains. In the problem demonstrated by Krier, a 76mm gun, the ratio for the seven perforated grains was $0.014 \text{ cm}^3/\text{cm}^2$. If the gun contained ball propellant the ratio would have been about 0.18 for balls having the same mass as the perforated cylinders. With such a spread in the geometry term, there would have to be a corresponding spread of an order of magnitude for the bulk temperature rise to keep the ignition energy fixed. In the 76mm gun simulation, Krier used a nominal bulk temperature rise of 30 R (17K) which presumes an ignition energy of 0.46 J/cm^2 .

When equivalent time to ignition was the criterion for evaluating the bulk temperature concept, a different conclusion was reached earlier.⁵ For a constant heating rate of 750 W/cm^2 a bulk temperature rise of 4K gives the same ignition time as a surface temperature rise of 155K for a multi-perforated grain. This should not be considered an indictment of the bulk temperature concept any more than the surface temperature concept. Both have absorbed the same energy at ignition, 0.17 J/cm^2 , a value low by reference to any experimental data.

Another popular criterion is that the ignition energy must approximate the energy content of the solid for steady-state combustion at the ignition pressure. For composite propellants, Derr and Fleming⁶ found a correlation of ignition delay with steady state burning rate for any value of the constant stimulus. If one assumes (1) burning rate to be a function of surface temperature only and (2) no subsurface reactions, the energy in the solid is

$$E = \rho c(T_s - T_o) \frac{a}{r} \quad (2)$$

where

T_s = surface temperature

a = thermal diffusivity

r = regression rate

5. C.W. Nelson, "Comparison of Predictions for Three Two-Phase Flow Codes", BRL Memorandum Report No. 2729, February 1977 (See also 13th JANNAF Combustion Meeting, September 1976). (AD #A037348)

6. R.L. Derr and R.W. Fleming, "A Correlation of Solid Propellant Arc Image Ignition Data", 10th JANNAF Combustion Meeting, Newport, RI, CPIA Publication 243, August 1973.

At a given burning rate, the energy should then be independent of the flux level; for the 0.5 cm/sec burning rate, the energy is about 5.4 J/cm². From the experimental data

q (W/cm ²)	420	210	125	163
E (J/cm ²)	2.7	3.8	3.8	4.5

Total energy (E) is neither constant nor simply dependent on heat flux (q). The steady burning energy is greater than the ignition energy by at least 30%.

For nitrocellulose propellants with the values used in earlier studies⁷, the steady state energy in the solid at a regression rate 0.75 cm/sec, is only about 1.3 J/cm². The DeLuca data⁸ reported 1.9 J/cm² energy at 210 W/cm² flux and .84 J/cm²/sec at 1050 W/cm² (extrapolated). These values are independent of pressure and therefore burning rate.

It is then not obvious how steady burning energy content and ignition are directly related.

NOVA code studies of the 155mm howitzer with M30 propellant have to date assumed a surface temperature ignition criterion of 450K, a value obtained by using the DeLuca⁸ data for the arc image ignition of the highly carbonized double base propellant with a slope of -2.0, that is,

$$t_{ig} \propto q^{-2.0}$$

where

$$\frac{d}{dt}(T - T_0) = 0$$

t_{ig} = ignition delay time
 q = heat flux.

Data on constant radiant flux stimulation of nitrocellulose propellants are available from the experiments of DeLuca et al⁸ for both arc image and laser sources. They found that some propellants exhibit a slope of -2.0 which means ignition energy and ignition surface temperature are independent of heating rate. In most cases the relationship was $t \propto q^{-1.6}$ which means some mechanisms other than simple surface heat absorption, presumably gas phase chemistry or in-depth absorption and heating. Because some of the propellant formulations absorb arc

-
7. L.H. Caveny, M. Summerfield, and C.W. Nelson, "Ignition Transients and Pressurization in Closed Chambers", BRL Memorandum Report No. 2558, November 1975. (AD #A017747)
 8. L. DeLuca, L.H. Caveny, T.J. Ohlemiller, and M. Summerfield, "Radiative Ignition of Double Base Propellants", Part I., 14 (?), p. 940-946, Part II, 14 (8), pp. 1111-1117 AIAAJ (1976).

radiation in-depth and because the propellant stimulus in the gun chamber is probably convective, it is reasonable to consider only the laser heating data which is practically all absorbed at the surface as convective flux would be.

Corroboration of these DeLuca data would be expected from arc image experiments of Hayes and Lenchitz⁹ who studied ignition of standard Army propellants. But they found slopes of -1.0 to -1.3.

Table I and Figure 1 compare the two sets of experimental data for uncatalyzed nitrocellulose propellants. The heat flux to the propellant surfaces in the granular bed has been computed (by NOVA code) to grow roughly linearly in time to about 1250 W/cm² at ignition. A column has been added to predict the ignition time at a constant flux of 840 W/cm² (an arbitrary but reasonable "average" value) assuming that the time vs flux data can be extrapolated with no change in slope, from a reference value of 210 W/cm², i.e.,

$$t_{ig}(840) = t_{ig}(210) \left(\frac{840}{210}\right)^n$$

where n is the reported slope.

The specific criterion is needed in the 155mm howitzer application for the triple base M30 propellant. Hayes and Lenchitz ignited both a usual film sample and a cylindrical grain in air with both a laser and an arc. The laser fluxes were too low to be applicable and the results too different from arc image results to be credible.

For M26 (double base) and M1 (single base) propellants, one should expect comparability with DeLuca's findings for Plastisol (double base) and M9 (single base) propellants respectively. Unfortunately, the comparison is weak. The slope difference for the double base is -1.5 vs -1.0, and for the single base -2.0 vs -1.0.

Russian radiant flux data reports ignition of nitrocellulose base propellant at low flux. Over the range 5-40 W/cm² the slope was -1.6, consistent with DeLuca, but of only limited supporting value.

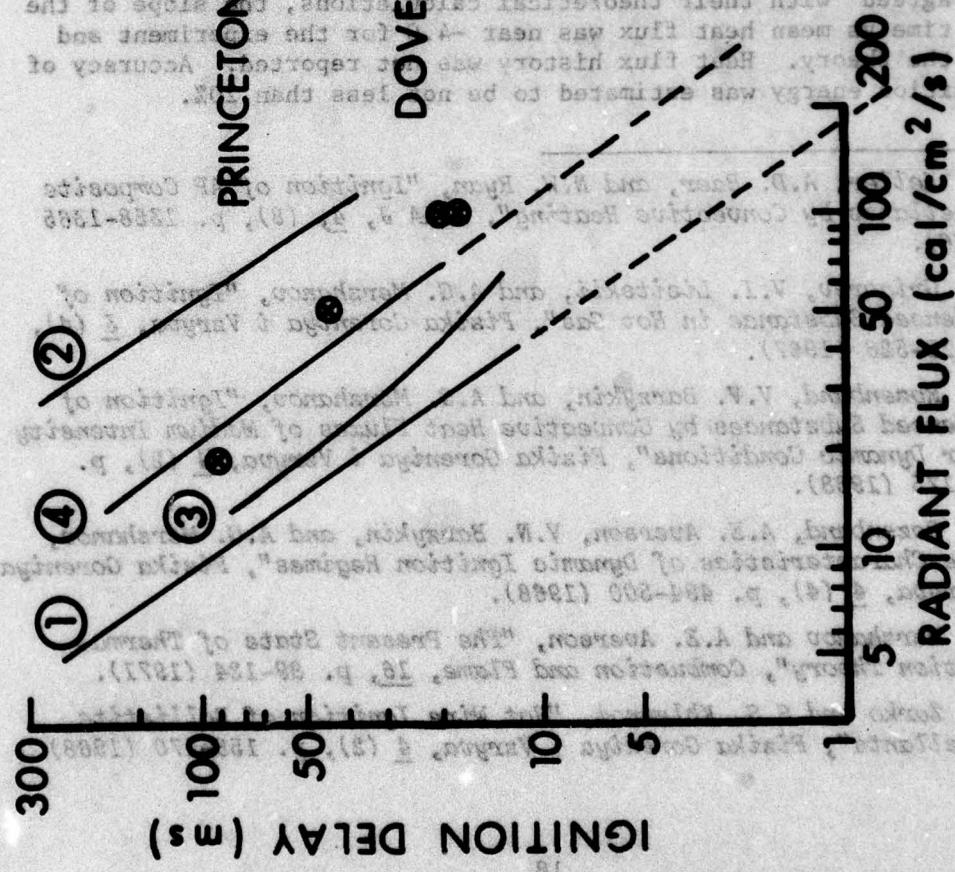
Given that this data must somehow yield an estimate of ignition time vs flux level, it seems that the most likely candidate is the data of DeLuca for the laser heating in air with a slope of -1.6 and a predicted delay of 1.0 ms at 840 W/cm² flux.

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9. E. Hayes and C. Lenchitz, "The Thermochemistry of Ignition", BRL Report No. 1833, ed. E. Freedman, K.J. White, I.W. May, 1976.
(AD #A016902)

TABLE I. NC BASE PROPELLANTS IGNITION

<u>Propellant</u>	<u>Investigator</u>	<u>Radiation Source</u>	<u>Slope</u>	<u>t₁₈ @210W/cm² (ms)</u>	<u>Predicted t₁₈ @840W/cm² (ms)</u>
M9	DeLuca	Laser, N ₂	-2.0	8	0.5
Plastisol	DeLuca	Laser, N ₂	-1.5	17	2.1
Plastisol, 0.2ZC	DeLuca	Arc, N ₂	-1.5	44	5.5
Plastisol, 0.2ZC	DeLuca	Laser, Air	-1.6	9	1.0
Plastisol, 0.2ZC	DeLuca	Laser, N ₂	-1.6	8	0.9
Plastisol, 0.2ZC	DeLuca	Arc, N ₂	-1.5	20	2.5
Plastisol, 0.2ZC	DeLuca	Laser, Air	-1.6	9	1.0
M30	Hayes	Laser, N ₂	-1.6	8	0.9
M26	Hayes	Arc, N ₂	-1.3	7	0.4
M11	Hayes	Arc, N ₂	-1.0	40	6.6
M11	Hayes	Arc, N ₂	-1.0	40	10.0
					47.5

Figure 1. Results of Radiant Flux Ignition Experiments.



For convective heating to ignition, Keller et al¹⁰ used a shock tunnel with a constant gas velocity inlet gas temperature to ignite composite propellant samples using heat fluxes from 125 to 630 W/cm^2 . The slope was -2.0 compared to -1.8 in the radiation furnace. Grigorev et al¹¹ ignited pyroxylin (nitrocellulose) cylinders by slow convective heating (10 sec) [convective heating coefficient, $h = .01 \text{ W/cm}^2/\text{°K}$ compared to a calculated $h = 0.4$ in the gun chamber]. Rosenband et al¹² ignited pyroxylin slabs with combustion product gases with mean heat fluxes $8-50 \text{ W/cm}^2$ although in each test the flux was monotonically increasing. Plotting ignition time vs mean heat flux yields a slope of -3 with pyroxylin products and -1.5 to -2.0 with composite propellant products. A scatter of 60% in ignition times was reported. Rozenband et al¹³ reported that in an increasing heat flux the effect of kinetic and thermophysical parameters on ignition time is much less than in a decreasing or constant heat flux. For rapidly increasing heat flux, the effect "may be unimportant and ignition lag can be determined solely by the dynamics of the external heat flow". Mershakov and Averson¹⁴ in a review paper observed that thermal theory and experiment agree to within about 10%. They also observed that in increasing heat flow, ignition times are nearly the same.

Hot wire ignition yields no more credible data. Zarko and Khlevnoi¹⁵ ignited ballistite (double base) propellant with an imbedded nickel wire using mean heat fluxes ranging from $20-125 \text{ W/cm}^2$. Although experimental results "agreed" with their theoretical calculations, the slope of the ignition time vs mean heat flux was near -4.0 for the experiment and -3.0 for the theory. Heat flux history was not reported. Accuracy of total ignition energy was estimated to be not less than 20%.

-
10. J.A. Keller, A.D. Baer, and N.W. Ryan, "Ignition of AP Composite Propellants by Convective Heating", *AIAA J.*, 4, (8), p. 1358-1365 (1966).
 11. Y.M. Grigorev, V.I. Lisitskii, and A.G. Mershakov, "Ignition of Condensed Substance in Hot Gas", *Fizika Gorenija i Vzryva*, 3 (4), p. 512-526 (1967).
 12. V.I. Rozenband, V.V. Barzykin, and A.G. Mershakov, "Ignition of Condensed Substances by Convective Heat Fluxes of Medium Intensity Under Dynamic Conditions", *Fizika Gorenija i Vzryva*, 4 (2), p. 171-175 (1968).
 13. V.I. Rozenband, A.E. Averson, V.N. Barzykin, and A.G. Mershakov, "Some Characteristics of Dynamic Ignition Regimes", *Fizika Gorenija i Vzryva*, 4 (4), p. 494-500 (1968).
 14. A.G. Mershakov and A.E. Averson, "The Present State of Thermal Ignition Theory", *Combustion and Flame*, 16, p. 89-124 (1971).
 15. V.E. Zarko and S.S. Khlevnoi, "Hot Wire Ignition of Ballistite Propellants", *Fizika Gorenija i Vzryva*, 4 (2), p. 158-170 (1968).

IGNITION DEFERRED (3m)

Given the data scatter and the difficulty of interpreting the slopes of -3 with little or no intermediate information, there seems little purpose in trying to apply it to the gun ignition sequence. A more useful course seems to be to adapt the radiant flux experimental data to the unsteady convective heating in the gun.

The Princeton laser-in-air data yields a formula

$$t_{ig} = 3.98 q^{-1.6} \quad (3)$$

where q is the heat flux in $\text{cal/cm}^2/\text{sec}$; t_{ig} is in sec. A typical NOVA calculation shows the heat flux to a grain to be roughly

$$q = 13.4 + 4.6 \times 10^5 (t-t_0) \quad (4)$$

where the heating starts at t_0 .

The mean heat flux is then

$$\bar{q} = \frac{1}{(t-t_0)} \int_{t_0}^t q dt \quad (4)$$

One route to computing the ignition delay would be the expression

$$t_{ig} = 3.98(\bar{q})^{-1.6} \quad (5)$$

although this may not work well for a rapidly changing flux such as

$$t_{ig} = 3.98 [13.4 + \frac{4.6 \times 10^5}{2} t_{ig}]^{-1.6} \quad (6)$$

Solve to find

$$t_{ig} = 0.8 \text{ ms},$$

whereas the surface temperature criteria predicts about 0.3 ms.

An alternative is to use the average value of $(q^{-1.6})$

$$(q^{-1.6})_{avg} = \frac{1}{t-t_0} \int_{t_0}^t q^{-1.6} dt$$

which for

$$q = a + b(t-t_0)$$

becomes

$$-1.6 \text{ ms}^{-1} \frac{1}{0.6bt} [(a+b(t-t_0))]^{-0.6}$$

When this expression is substituted into Eq. (4), which is then solved for ignition time

$$t_{ig} = 1.7 \text{ ms.}$$

This is a long time compared to 0.3 ms.

The heat flux estimates were taken from calculations which only went to the 0.3 ms ignition. The 0.8 ms estimate depends on extrapolation of the heat flux. One check is to calculate the flamespreading using the continuously calculated mean heat flux to decide if ignition criterion has been satisfied. Eq. (4) becomes

$$t_{ig} = 3.98 \left[\frac{1}{t-t_0} \int_0^t q d\tau \right]^{-1.6}$$

If $t > (t_{ig} + t_0)$, ignition has occurred.

A test case for these ignition studies can be constructed easily by modifying the input data for a 155mm howitzer calculation. The only changes are shown in Table II.

TABLE II. INPUT DATA CHANGES FOR TEST CASE (NOVA CODE)

	<u>155mm Howitzer</u>	<u>Test Case</u>
Projectile	Movable	Fixed
Chamber Length	82 cm	53 cm
Charge Length	53 cm	53 cm
Igniter Length	5 cm	15 cm

These changes are not enough to affect the flamespreading in most of the bed, but they do shorten the calculation.

Using the ignition delay criterion (-1.6 slope) reduces the calculated flamespreading rate markedly. Whereas the rate was about 0.27 mm/us for the fixed ignition temperature (450 K), it is only half that value with the average flux method. From a rate of 0.1 mm/us just outside the igniter input region it reaches 0.15 mm/us in a distance of 17 cm; thereafter it is approximately constant. The predicted propagation rate of the first absorption of heat by the grain decelerates from 0.6 mm/us near the igniter to 0.35 mm/us downstream. (The sound speed in the undisturbed air in the bed is 0.34 mm/us.) Ignition delay time increases from 0.4 ms at the igniter region to 1.9 ms well downstream.

All these trends can be seen in Figure 2 where they are compared with the ignition speed predicted with the fixed surface temperature criteria of 810 and 1500 R.

This ignition criterion and transient regression cannot be simultaneously applied. The transient regression assumes that the temperature profile in the solid at ignition is the initial condition for the combustion. This ignition criterion ignores the temperature profile and looks only at the rate of heat absorption. The gasification period of the flame development is not a straightforward conduction problem and the temperature profile cannot be so computed. Until a model for the flame development can give initial conditions for the combustion, a quasi-steady pressure dependence is the only credible formula.

III. INTEGRAL vs FINITE DIFFERENCE SOLUTION

Heating of the solid to ignition requires a solution to the transient heat conduction equation. Because the boundary condition of heat flux cannot be specified, a numerical solution is dictated. A choice must be made between "exact" finite difference and approximate techniques. Both have been reasonably well studied; it remains only to judge their relative errors for the problem at hand.

It is widely recognized that the FDE solutions can come almost as close to the exact solution as the user is willing to shrink his discretization. Kooker and Nelson¹⁶ found that for an intense heating problem the invariant imbedding technique estimated surface temperature within 1% of the exact solution.

A well known approximate method for transient conduction is the integral method of Goodman which has a surface temperature error of 9% for a parabolic and 2% for a cubic profile when the incident heat flux is constant¹⁷. Because the heat flux in the propellant bed is computed to be approximately a linear ramp in time, it would be worthwhile to estimate the error for these profiles.

The form of the exact solution is given by Carslaw and Jaeger¹⁸ as:

-
16. D.E. Kooker and C.W. Nelson, "Numerical Solution of Solid Propellant Transient Combustion", ASME Paper 77-HT-17, August 1977.
 17. T.R. Goodman, "The Heating of Slabs with Arbitrary Heat Inputs", J. Aerospace Sciences, 187-188, March 1959.
 18. H.S. Carslaw and J.C. Jaeger, "Conduction of Heat in Solids", Oxford Press, 2nd ed., pp. 76, 1959.

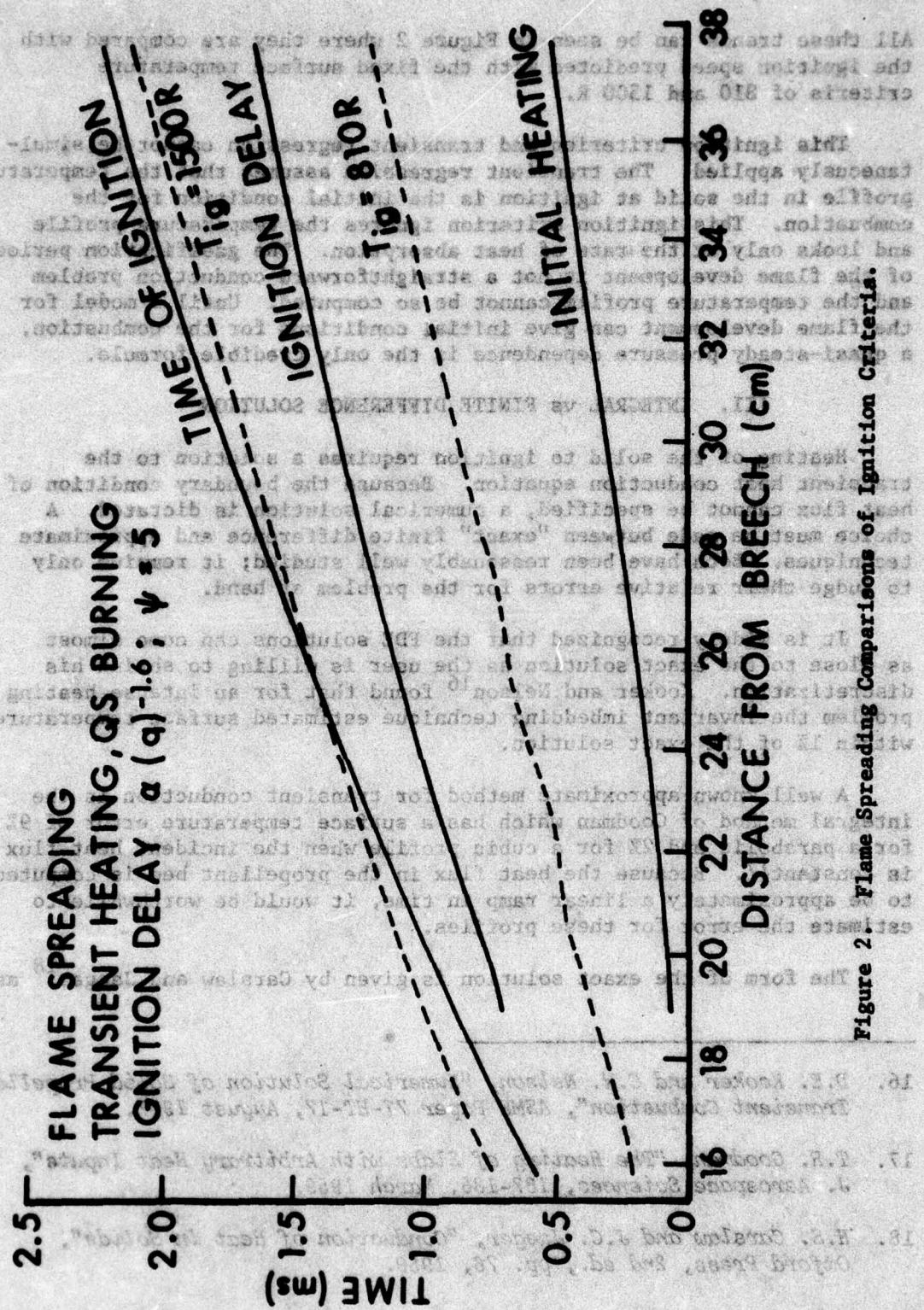


Figure 2. Flame Spreading Comparisons of Ignition Criteria.

$$T_s = \sqrt{\frac{a}{\lambda^2 \pi}} \int_0^t \frac{q(t-\tau)}{\sqrt{\tau}} d\tau + T_o \quad (9)$$

where

T_s = surface temperature
 λ = thermal conductivity
 t = time

T_o = initial temperature

For a linear ramp flux

$$q = q_o + at \quad ,$$

this becomes

$$T_s = \sqrt{\frac{a}{\lambda^2 \pi}} [2q_o t + \frac{4}{3} at^{3/2}] + T_o \quad (10)$$

For the cubic profile where

$$T_s = T_o + \frac{q}{3\lambda} \sqrt{\frac{12a}{q}} \int_0^t q(\tau) d\tau$$

the surface temperature becomes

$$T_s = T_o + \sqrt{\frac{4a}{3\lambda^2}} \sqrt{qt(q_o + \frac{at}{2})} \quad (11)$$

For the parabolic profile the surface temperature becomes

$$T_s = T_o + \sqrt{\frac{3}{2}} \frac{a}{\lambda^2} qt (q_o + \frac{at}{2}) \quad (12)$$

It can be seen that the parabolic profile will predict a higher surface temperature by the ratio $\sqrt{\frac{3}{2}}$ or about 6% of the temperature rise.

Comparison with the exact solution can be obtained by using the typical gun heating rate

$$q = 56 + 1.9 \times 10^6 t \text{ (W/cm}^2\text{)} \quad (13)$$

which estimates an error of 15% for the parabolic profile and 8% for the cubic profile. For the more useful estimate of error in time to reach a specified surface temperature the estimates are 10% for the parabolic and 6% for the cubic.

The FDE invariant imbedding technique produces an estimated error of less than 1%.

Since there is little difference in computational effort between the parabolic and cubic approximations it is not surprising that the cubic is already used by Gough and Kuo. And if surface temperature accuracy were the only criterion, it would be rather straight forward to make the trade-off between accuracy and efficiency. If quasi-steady combustion is assumed after ignition, surface temperature is probably an adequate test because the ignition criterion will probably be dominated by the surface behavior.

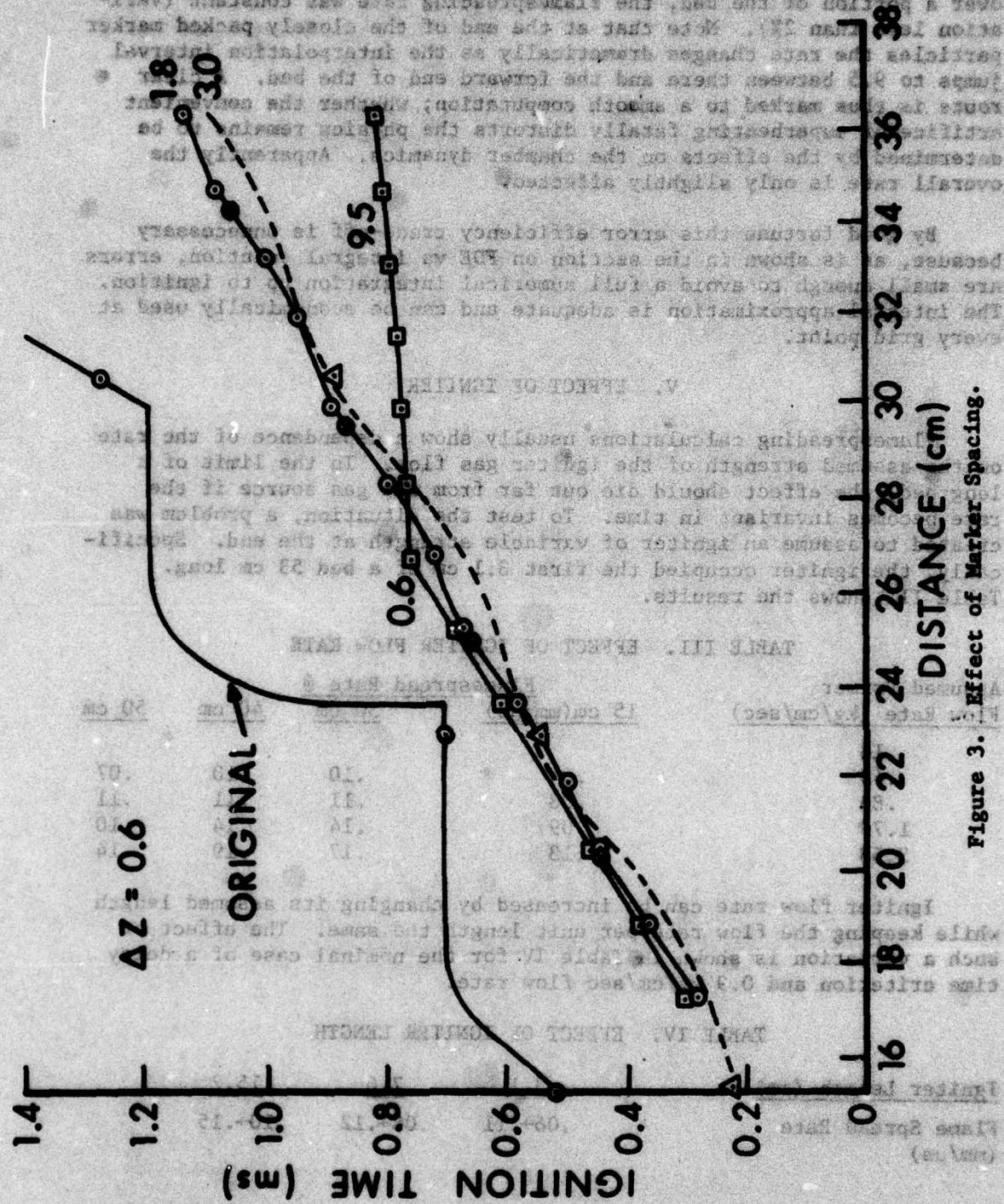
If, however, transient combustion is to be calculated with a thermal theory model, it is also necessary to have an accurate temperature-distance profile in the solid. For this reason, the only code coupling transient combustion to the flow uses an FDE technique throughout. For a constant heating problem, the error in the temperature vs distance calculation can be found by direct calculation of both the exact and the integral solutions. The error of 2.3% at the surface represents a temperature error of 8.3 K for a problem of heating at 840 W/cm^2 for 0.8 ms. The temperature error decreases with distance to a distance of $1.5\sqrt{at}$ at which point the two solutions cross over such that the approximate solution is less than the exact solution and the error begins to grow although the values of both are small.

IV. NUMERICAL PROBLEMS

NOVA economizes on storage and running time by using marker particles (fewer than grid points) to compute the ignition and transient combustion. Matching the solutions there to the flow field at the grid mesh points requires interpolation in both directions. In the limit as the marker particle spacing approaches the mesh spacing, interpolation errors shrink toward zero.

The original scheme computed grain temperatures in the flow mesh by linear interpolation between the markers. Surface temperature computation of the marker stopped at ignition. The result was a leap-frog flamespreading as shown in Figure 3. The problem was the temperature interpolation. An artifice was then incorporated which continued to heat the ignited marker as an inert body without taking the heat from the gas. It was hoped thereby to smooth the interpolation without affecting the energy balance in the gas. Things improved considerably as shown by the curve in Figure 3 marked 3.0. By comparison the grid spacing was 0.6, leaving five (5) grid points between marker particles. Even this flamespreading is ragged; the local flamespreading rate varies

Figure 3. Effect of Marker Spacing.



over a factor of two. With the marker spacing equal to the grid spacing over a portion of the bed, the flamespreading rate was constant (variation less than 2%). Note that at the end of the closely packed marker particles the rate changes dramatically as the interpolation interval jumps to 9.5 between there and the forward end of the bed. A clear route is thus marked to a smooth computation; whether the convenient artifice of superheating fatally distorts the physics remains to be determined by the effects on the chamber dynamics. Apparently the overall rate is only slightly affected.

By good fortune this error efficiency trade-off is unnecessary because, as is shown in the section on FDE vs integral solution, errors are small enough to avoid a full numerical integration up to ignition. The integral approximation is adequate and can be economically used at every grid point.

V. EFFECT OF IGNITER

Flamespreading calculations usually show a dependence of the rate on the assumed strength of the igniter gas flow. In the limit of a long bed, the effect should die out far from the gas source if the rate becomes invariant in time. To test the situation, a problem was created to assume an igniter of variable strength at the end. Specifically, the igniter occupied the first 3.1 cm of a bed 53 cm long. Table III shows the results.

TABLE III. EFFECT OF IGNITER FLOW RATE

Assumed Primer Flow Rate (kg/cm/sec)	Flamespread Rate @			
	15 cm(mm/us)	30 cm	40 cm	50 cm
.18				
.45	.04	.10	.10	.07
.89	.08	.11	.11	.11
1.79	.09	.14	.14	.10
3.58	.13	.17	.19	.14

Igniter flow rate can be increased by changing its assumed length while keeping the flow rate per unit length the same. The effect of such a variation is shown in Table IV for the nominal case of a delay time criterion and 0.9 kg/cm/sec flow rate.

TABLE IV. EFFECT OF IGNITER LENGTH

Igniter Length (cm)	3.1	7.6	15.2
Flame Spread Rate (mm/us)	.06+.11	.08+.12	.10+.15

Because gun charges are usually not long compared to igniter length, any calculation of flamespreading rate will be sensitive to igniter assumptions. Such unpleasant news means that assumptions about the igniter must either be carefully formulated from experimental data or the flow rate will become just another arbitrary parameter. That is not to say, however, that the gun calculation will be otherwise useless; some important ballistics variables seem insensitive to flamespreading rate. How insensitive is not immediately assessable.

VI. COMPARISON WITH MEASUREMENTS

East et al¹⁹ have made the few measurements of the flamespreading rates in naval gun propellant beds. Their data are shown in Table V which shows the major variables expected to affect the flamespreading rate.

TABLE V. MEASURED FLAMESPREADING RATES

Nominal Chamber Diameter	Propellant	Grain Length (mm)	Diameter (mm)	Burning Rate 7 MPa (mm/sec)	Flamespread Rate (mm/ μ s)
76 mm	M6	17	7.4	13	.36-.48
76 mm	M6+2	17	7.4	12	.35-.39
127 mm	M26	30	14	11	.44
127 mm	NACO	13	6.9	4	.30

It may well be noted that there are experimental uncertainties in igniter gas flow, in flame front location at only a few discrete locations, and in distinguishing the ignition front from the luminous gas penetration ahead of the front. Nevertheless, it is clear that the observed rates are much higher (0.4 vs 0.2) than those calculated under the ignition delay criterion. The only calculations which estimate these observed rates are those under a surface temperature criterion of about 150°C.

For the 155mm howitzer propellant bed which formed the basis for these calculations, the propellant geometry and burning rates are much the same as the 127mm gun. One would expect the flamespreading rate to vary only in relation to the degree by which the igniter gas flow differs.

19. J.L. East and D.R. McClure, "Studies of Ignition and Combustion in Naval Guns", Proceedings of the 12th JANNAF Combustion Meeting, Newport, RI, August 1975 (See also 11th JANNAF Combustion Meeting).

That the calculations predict a lower than measured rate suggests that some aspect has been incorrectly represented. The ignition criterion based on a delay seems supportable by experimental data although below the expected heat flux range. The most likely suspect then are the heat flux calculation, and the transient regression rate just after ignition. Because there have been no direct measurements of these variables, their contribution must still be crudely estimated. For now, the surface temperature criterion gives more credible answers despite its apparent deficiencies.

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VII. GUN APPLICATION

Since the motivation for these studies is improvement in the ability to simulate gun ballistics, the ignition criterion was tested in a simulation of a 155mm howitzer. The base case was a bore size charge simulation reported in Reference 5. It was this that formed the base for the flamespreading calculations. An additional calculation simulated averaging the function ($q^{-1.6}$) by doubling the computed ignition delay time from the ($q^{-1.6}$) as used in Equation (1). Table V compares the predictions. But by this time the temperature has dropped to only 10% of the surface temperature. Although it is not straightforward to estimate the effect of such an error on the non-linear conduction problem of combustion, such errors are often accepted in combustion calculations.

For many cases, however, there is little practical benefit to combining an integral method for heating and an FDE method for transient combustion. The only attractive combination would be a condition wherein the ignition time is at least comparable to combustion time and a surface temperature ignition criterion is used. Under any other conditions the useful combination would be either (1) integral heating--ignition delay criterion--quasi-steady combustion, or (2) FDE heating--surface temperature criterion--transient combustion.

For a nominal case with igniter flow rate 0.9 kg/cm/sec over 15 cm, comparisons are shown in Table VI of ignition propagation rates in the first 10 cm ahead of the igniter.

TABLE VI. EFFECT OF INTEGRATION METHOD

	Surface Temperature Criterion (450K)	Ignition Delay Criterion
FDE	0.21 (mm/ μ s)	0.11 (mm/ μ s)
Integral	0.23 (mm/ μ s)	0.13 (mm/ μ s)

These differences between the FDE and integral method are small enough to justify using the integral method.

TABLE VII. EFFECT ON BALLISTICS

<u>Commodity</u>	<u>Experimental</u>	<u>Base Case</u>	<u>Revised Ignition</u>	<u>Doubled Revised Ignition</u>
Flame Spread Rate (mm/ μ s)	unknown	0.35-0.44	0.2-0.3	.17-.21
Peak Pressure (MPa)	235	293	268	265
Pressure Differences (MPa)*	-40/35	-73/48	-75/51	-67/53
Muzzle Velocity (m/s)		790	768	NC

* - First number is first negative difference; second is second positive.

An immediate conclusion drawn from Table VII is that the ignition criterion does not appreciably affect pressure difference amplitudes. The results also show that the reduction of 50% in flame speed reduces peak pressure by only 10%.

It should not be considered significant in this study that all the simulations over-predict the actual ballistics. This condition could be remedied by adjusting the mostly guessed-at bore resistance function. The trends should remain the same.

VIII. CONCLUSIONS

- (1) Laser-in-air radiant heating data yield the most credible basis for an ignition criterion.
- (2) An integral method based on a cubic temperature profile is a satisfactory approximation for calculating transient conduction in the solid. Only a small error is thereby caused in flamespreading rate.
- (3) Calculated flamespreading rate is sensitive to the assumed ignition criterion.
- (4) Experimental data on ignition, transient regression rates, and flamespreading rates are too scarce to form a basis for definitive conclusions on the correct methods.

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